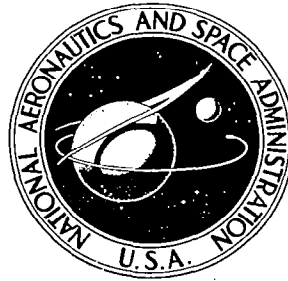


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**APPLICATION OF A GENERAL STRESS,  
STRAIN-RATE, TEMPERATURE CORRELATION  
FOR WELDED THICK-WALLED N-155 TUBES  
UNDERGOING SECONDARY CREEP  
FROM INTERNAL PRESSURE**

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*Cleveland, Ohio 44135*



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16. Abstract <p>A correlation equation relating creep deformation of tubes to a function of stress and temperature is presented. The equation was fitted to experimental N-155 tube test data with a multiple linear regression analysis. A design procedure is proposed and applied to the design of a heat-exchanger tube. Design calculations showed that a welded, thick-walled, N-155 heat-exchanger tube having an outside diameter of 0.635 cm and an inside diameter of 0.391 cm was satisfactory for 10 000-hr service life at 1089 K (1500<sup>0</sup> F), with 10.34 MN/m<sup>2</sup> (1500 psi) internal pressure, and with a limitation of 1 percent on creep deformation during service life.</p>			
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APPLICATION OF A GENERAL STRESS, STRAIN-RATE, TEMPERATURE  
CORRELATION FOR WELDED THICK-WALLED N-155 TUBES UNDER-  
GOING SECONDARY CREEP FROM INTERNAL PRESSURE

by Richard E. Morris  
Lewis Research Center

SUMMARY

Helium-to-air heat-exchanger tubes in the engines of a mobile nuclear propulsion system must operate with high internal pressures at high temperatures. Under these conditions creep will occur continuously during the operating life of the heat exchanger. A method was needed for predicting the creep deformation of tubing as a function of time in operation so that the total creep deformation accumulated during a design lifetime could be limited to some small acceptable value.

A plot of some experimental tube test stress as a function of creep-rate data was available for welded N-155 thick-walled tubes. A procedure was needed for the interpolation and the extrapolation of the experimental data for use in design.

This report presents a correlation of the experimental N-155 tube strain rate data as a function of stress and temperature. Material constants in the equation were optimized with a multiple linear regression analysis. The correlation equation was investigated graphically and found to be representative of the experimental data.

The ranges of data correlated were temperature, 1061 to 1234 K (1450° to 1760° F); stress, 16.2 to 77.6 MN/m<sup>2</sup> (2.35 to 11.25 ksi); ratio of tube outside to inside diameter, 1.154 to 1.623; and test time, 140 to 1857 hr. Experimental strain rates varied from  $44.5 \times 10^{-6}$  to  $584 \times 10^{-6}$  cm/(cm)(hr).

A design procedure is proposed and applied to a design problem. An N-155 tube is designed for use in a heat exchanger. Calculations showed that a thick-walled, welded N-155 heat-exchanger tube could be designed to operate at 1089 K (1500° F) with a stress of 10.34 MN/m<sup>2</sup> (1500 psi) for a 10 000-hr lifetime. Total growth in tube diameter is less than 1 percent in 10 000 hr. The actual stress in the tube is less than the 10 000-hr creep-rupture stress by a factor of 1.85.

The procedure presented for the analysis of welded, thick-walled, N-155 heat-exchanger tube test data is applicable for the analysis of tube test strain-rate data obtained for other tube materials.

## INTRODUCTION

High-temperature heat exchangers are required for use in engines for mobile nuclear propulsion systems. These engines will be operated with hot helium gas at temperatures of 1000 to 1150 K, pressurized at 7 to 14 MN/m<sup>2</sup>.

Under these conditions of operation, the heat-exchanger tubes will creep throughout the operating life of the engine. Loss of coolant through rupture of the tubing must be avoided. The total amount of creep deformation of the tubing at the end of life must be limited in the design of the heat exchanger.

A report on welded N-155 tubing (ref. 1) includes a parametric correlation of stress as a function of the Larson-Miller parameter. This parameter is a function of time and temperature. The strain-rate data were based on diameter measurements taken before and after the tests. Thus, it was assumed that primary creep was negligible and that the strain rate was uniform throughout the lifetimes of the tubes tested. Strain rates were plotted in a graph of stress as a function of strain rate at constant temperature. No method was provided for interpolation or extrapolation of the data for use in design calculations.

One purpose of this report is to provide a systematic procedure for the analysis of strain-rate data for use in the design of thick-walled tubes operating under conditions of internal pressure and high temperature such that creep continues throughout the operating life of the tubes.

Reference 2 provided a system of equations relating the stresses and strain rates in thick-walled tubes undergoing secondary creep from static internal pressure at constant temperature. This report describes an application of equations from the reference report. Correlation equations are used to provide creep strain-rate data for welded N-155 tubes which may be used for the accurate interpolation or extrapolation of strain-rate data over a range of temperature and stress in the design of heat-exchanger tubing.

## SYMBOLS

A	material constants, $\text{hr}^{-1} (\text{Nm}^{-2})^{-n}$
a	inside radius, cm
b	outside radius, cm
$\Delta H$	apparent activation energy, J/mole
$N_1$	safety factor, $\bar{\sigma}_u/\bar{\sigma}_a$
$N_2$	safety factor, $t_u/t_a$

n	stress exponent
P	Larson-Miller parameter
p	pressure, NM <sup>-2</sup>
R	gas constant, 8.3143 J/(mole)(K)
s	standard deviation
T	temperature, K
t	time, hr
x	variable
$\dot{\epsilon}$	creep rate, hr <sup>-1</sup>
$\bar{\dot{\epsilon}}$	equivalent creep rate, $\frac{\sqrt{2}}{3} \left[ (\dot{\epsilon}_r - \dot{\epsilon}_\theta)^2 + (\dot{\epsilon}_\theta - \dot{\epsilon}_z)^2 + (\dot{\epsilon}_z - \dot{\epsilon}_r)^2 \right]^{1/2}$ , hr <sup>-1</sup>
$\rho$	b/a
$\sigma$	stress, Nm <sup>-2</sup>
$\bar{\sigma}$	equivalent stress, $\frac{1}{\sqrt{2}} \left[ (\sigma_r - \sigma_\theta)^2 + (\sigma_\theta - \sigma_z)^2 + (\sigma_z - \sigma_r)^2 \right]^{1/2}$ , Nm <sup>-2</sup>

Subscripts:

a	inside diameter
b	outside diameter
c	calculated value of variable
d	design
i	experimental value of variable
m	melting
r	radial
u	ultimate
z	axial
$\theta$	circumferential

## ANALYSIS

The empirical strain-rate equation assumed in reference 2 is

$$\dot{\epsilon} = A \bar{\sigma}^n e^{-\Delta H/RT} \quad (1)$$

TABLE I. - CHEMICAL COMPOSITION OF N-155<sup>a</sup> SPECIMENS

Specimen size				Heat	Sample	Carbon	Manganese	Silicon	Phosphorous	Sulfur	Chromium	Nickel	Molybdenum	Cobalt	Columbium or tantalum	Tungsten	Nitrogen
Diameter		Wall thickness															
Composition, wt %																	
cm	in.	cm	in.														
----	----	----	----	----	AMS 5585	0.08 to 0.16	1.00 to 2.00	<sup>b</sup> 1.00	<sup>b</sup> 0.030	<sup>b</sup> 0.030	20.0 to 22.50	19.00 to 21.00	2.50 to 3.50	18.50 to 21.0	0.75 to 1.25	2.00 to 3.00	0.10 to 0.20
0.953	0.375	0.064	0.025	C-3110	As- received 7 after test	0.14 .13	1.65 1.67	0.60 .65	0.015 .016	0.003 .009	21.02 21.04	20.04 19.91	3.01 3.03	19.13 19.15	1.20 1.24	2.64 2.88	0.11 .15
0.635	0.250	0.122	0.048	C-4836	As- received 24 after test	0.09 .10	1.43 1.63	0.42 .49	0.013 .015	0.006 .010	21.27 20.54	19.62 19.75	3.00 2.97	19.66 19.63	0.98 1.12	2.48 2.65	0.17 .15

<sup>a</sup>Iron base alloy.<sup>b</sup>Maximum.

TABLE II. - WELDED N-155 TUBE DATA

Specimen size <sup>a</sup>	Temperature, K	Equivalent stress in bore, MN/m <sup>2</sup>	Equivalent strain rate in bore, cm/(cm)(hr)	Lifetime, hr
A	1144	47.4	219.0×10 <sup>-6</sup>	410
A	1144	47.4	201.0	385
A	1152	34.5	87.5	1047
A	1152	34.5	77.3	1003
A	1152	34.5	87.7	1068
A	1152	34.5	83.1	1017
A	1061	77.6	44.5	1857
A	1061	77.6	58.3	1762
A	1061	77.6	45.1	1619
A	1222	19.4	177.5	420
A	1153	34.5	60.9	1060
A	1153	34.5	88.5	1094
A	1153	47.4	404.0	280
A	1153	47.4	293.0	282
A	1214	20.3	106.0	545
A	1214	20.3	175.0	581
A	1214	30.9	424.0	140
A	1214	30.9	584.0	147
B	1178	24.7	74.2	1223
B	1178	24.7	68.7	1137
B	1178	24.7	74.2	1121
B	1178	24.7	64.8	1246
B	1214	16.2	72.4	1381
B	1214	16.2	71.5	1347
B	1214	16.2	68.1	1360
B	1234	16.2	173.5	590
B	1234	16.2	172.0	623
B	1234	24.7	540.0	138
B	1234	24.7	559.0	157

<sup>a</sup>Specimen size A, 0.953-cm o.d., 0.064-cm wall thickness.

Specimen size B, 0.635-cm o.d., 0.122-cm wall thickness.

This equation contains material constants  $A$ ,  $n$ , and  $\Delta H$  to be evaluated.

The chemical composition of the two heats of tubing tested is given in table I. Stress and strain-rate data from reference 1 are listed in table II. These data were correlated with equation (1) by using a computer code called Rapier (ref. 3), a multiple linear regression analysis. The code evaluates the material constants, calculates the standard deviations, and determines the level of confidence in the effectiveness of equation (1) as a model for the experimental data.

With the constants evaluated, the strain-rate equation (1) for N-155 becomes

$$\dot{\epsilon} = 7.517 \times 10^8 \bar{\sigma}^{-3.310} e^{-396\,500/RT} \quad (2)$$

The apparent activation energy  $\Delta H$  calculated in the computer program was 396 500 J/mole with the standard deviation  $s$  of 21 000 J/mole. This compares favorably with a value of 419 000 J/mole given by Conrad, Bennett, and White (ref. 4).

The stress exponent  $n$  has a calculated value of 3.310 with  $s = 0.204$ . This value falls in the range of 2 to 4 for alloys, as given by Garofalo (ref. 5).

Values of strain rate were calculated for each set of test conditions by using equation (2). A standard deviation of 21.4 percent was obtained from the variance between calculated and experimental values for strain rate. This indicated that the empirical equation provided a reasonable fit with the experimental data.

Figure 1 is a plot of the experimental strain-rate data. The solid lines plotted were

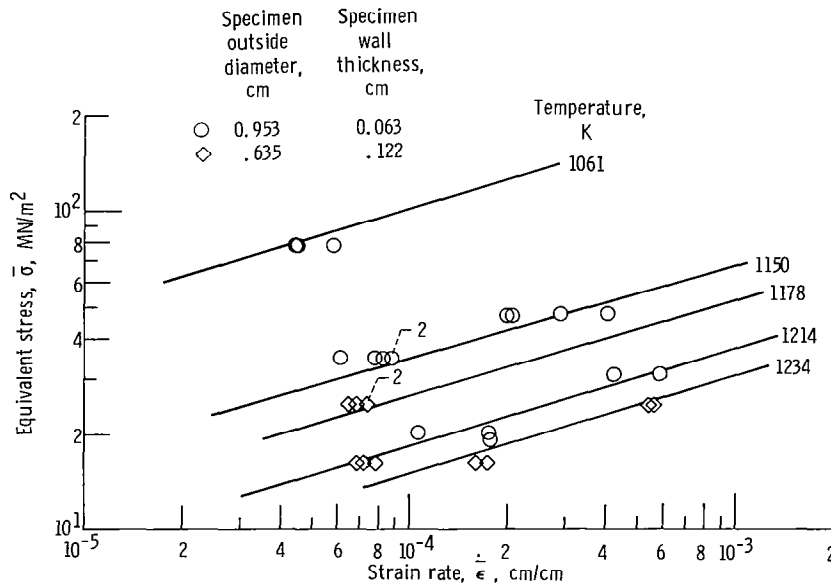


Figure 1. - Equivalent stress as a function of strain rate. Creep rate  $\dot{\epsilon} = 7.517 \times 10^8 \bar{\sigma}^{-3.310} e^{-396\,500/RT}$ , where  $R$  the gas constant and  $T$  is temperature; standard deviation  $s = 21.4$  percent.



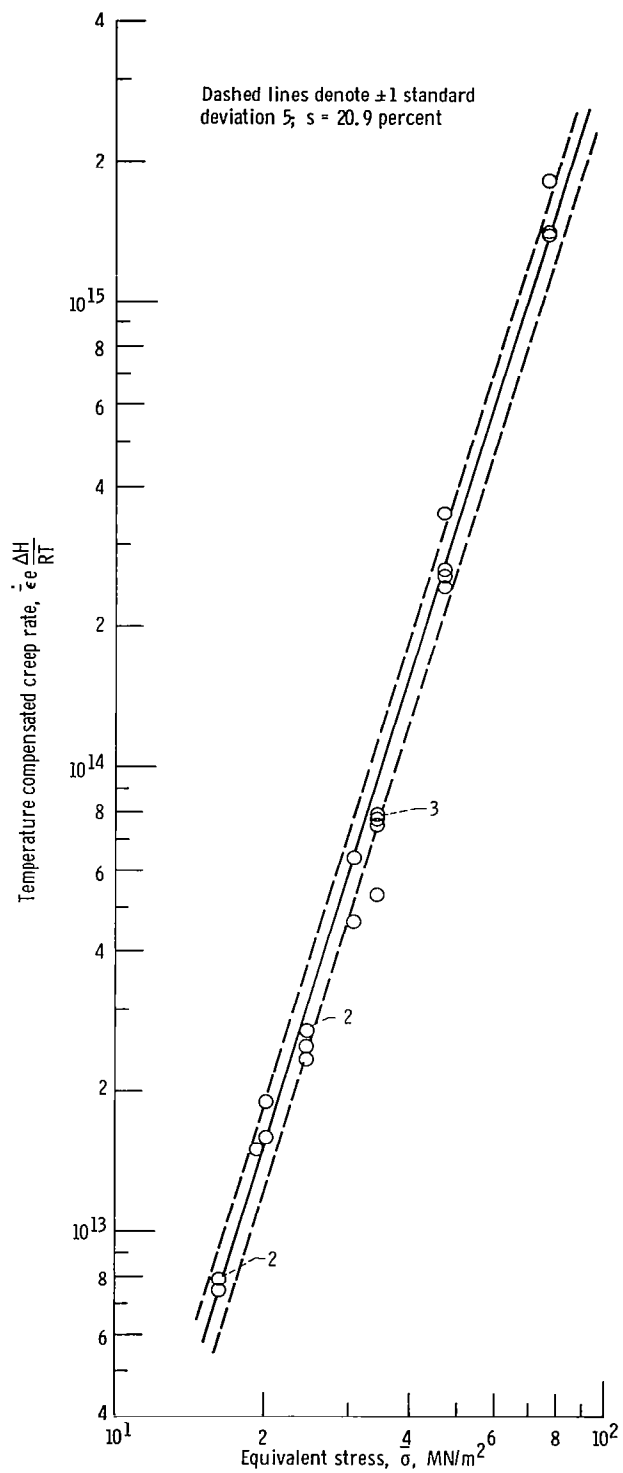


Figure 2. - Temperature-compensated creep rate as function of stress for constant stress and temperature data.

obtained from equation (2) for the temperatures indicated. The graph verifies the correlation of the experimental data.

Figure 2 is a graph of temperature-compensated creep rate as a function of stress. The solid line has a slope of 3.310, which is the value for the stress exponent  $n$ .

Figure 3 is a plot of strain rate divided by the stress function against the inverse of absolute temperature. Variation in observed values of strain rate may result from factors other than experimental errors. The apparent activation energy for creep may not be constant over the range of test temperatures. Creep rates vary with grain sizes (ref. 3), which may vary with different heats and with time at temperature. Metallurgical changes with time at temperature such as the precipitation of carbides within the grains may affect creep rates. No source of variation was identified from the graph. The scatter of the experimental data appears to be random and to have resulted from experimental errors.

Figures 1 to 3 verify that the empirical correlation equation is representative of the experimental data.

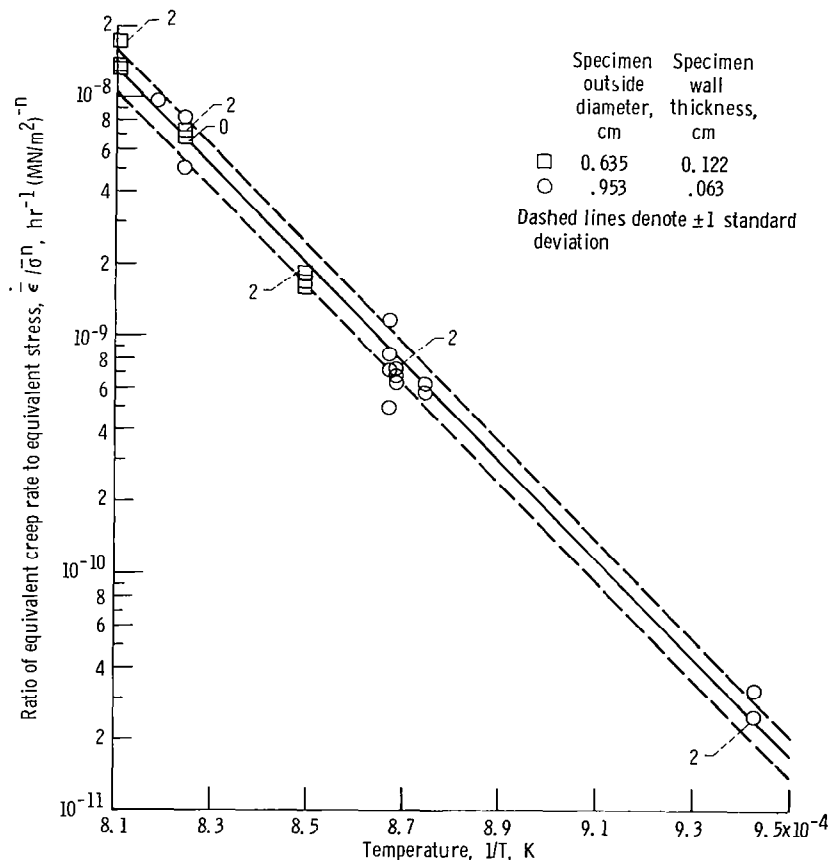


Figure 3. - Ratio of creep rate to stress as function of temperature. Equivalent creep rate  $\dot{\epsilon} = 7.517 \times 10^8 \bar{\sigma}^{3.31} e^{-39600/T}$ ; gas constant  $R = 8.3143 \text{ J/(mole}^\circ\text{K)}$ .

Figure 4 is a graph of equivalent stress at the inside radius of each specimen plotted against the Larson-Miller parameter. Curves are presented for rupture stress (solid line) and for the stress for 1-percent creep in 10 000 hr (long and short dash line). Data for the rupture line were obtained from reference 1. The standard deviation of the data, 4.65 percent, was based on the variance of stress between the values predicted by the rupture line and the experimental values.

For each stress in figure 4, there is one temperature that will produce a strain rate of  $1.0 \times 10^{-6}$  cm/(cm)(hr), the rate equivalent to 1-percent strain in 10 000 hr. The long and short dash line passes through a series of such points. Rupture-stress data points were calculated by using the equation for the equivalent stress at the inside radius of tubes tested. The plotted line therefore refers to 1-percent equivalent strain at the same position where the equivalent stress was calculated, in the bore of the tube.

The correlation equation (2) and the plot of stress against the Larson-Miller parameter in figure 4 may be used to design heat-exchanger tubes for use at high temperature and pressure to meet creep-deformation limitations and to avoid creep-rupture. The correlation equation is useful in the interpolation and the extrapolation of the experimental N-155 tube data for design applications requiring low creep strain rates at stresses less than  $40 \text{ MN/m}^2$  (5.8 ksi).

When similar experimental data are provided for other materials, the correlation procedure presented may be used in the design of heat-exchanger tubes fabricated from those materials.

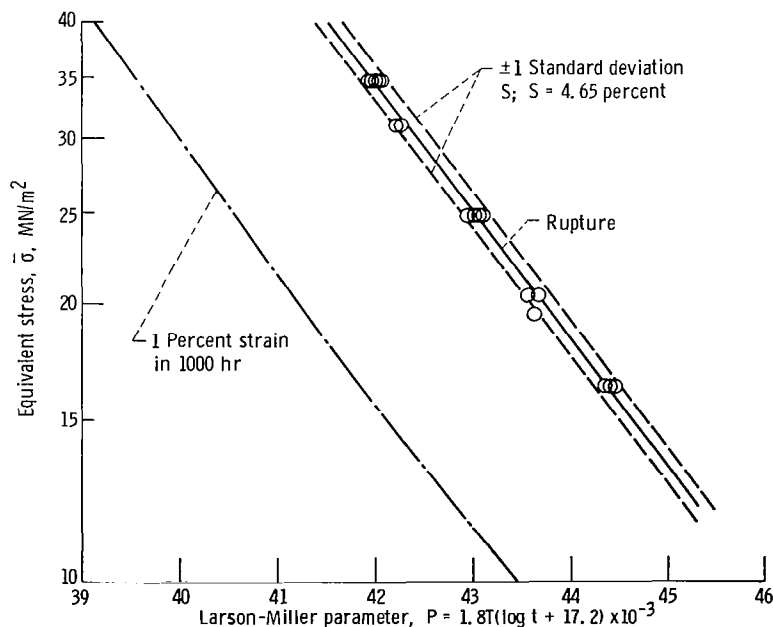


Figure 4. - Equivalent stress as function of Larson-Miller parameter for rupture and for 1 percent strain in 10 000 hours.

## APPLICATION

The design conditions for a sample helium-to-air heat-exchanger tube are given as follows:

Internal gas . . . . .	helium
External atmosphere . . . . .	air
Internal pressure, MN/m <sup>2</sup> (psi) . . . . .	10.34 (1500)
Operating temperature, K (°F) . . . . .	1089 (1500)
Design lifetime, hr . . . . .	10 000
Maximum allowable tube growth in design lifetime, percent . . . . .	1
Minimum allowable safety factor based on creep-rupture stress, N <sub>1</sub> . . . . .	1.50

The following design procedure for heat-exchanger tubing using welded N-155 tubes is proposed:

- (1) Select a tube geometry compatible with the thermodynamic design of the heat exchanger.
- (2) Calculate the growth rate of the tubing under design conditions and check total creep with the 1.0-percent limit on growth in diameter. If the creep limit is exceeded, adjust the geometry and repeat.
- (3) Determine the creep-rupture stress in the tube for the operating conditions.
- (4) Calculate the equivalent stress in the bore of the tube.
- (5) Take the ratio of the creep-rupture stress for the design conditions to the equivalent stress in the bore of the tube. If this ratio is less than 1.50 adjust the geometry and repeat from step (1).
- (6) When all conditions are met the design is satisfactory for the creep deformation and the creep-rupture requirements.

Conditions that remain to be considered include thermal stresses, corrosion effects from high-velocity air, vibrations, and thermal cycling. These unknown factors are assumed to be covered by the safety factor selected in step (5).

As an example of this design procedure, calculations will be made for an N-155 heat-exchanger tube for the design conditions given using formulas from reference 2.

The tube geometry selected in step (1) is a 0.635-cm outside diameter and a 0.122-cm wall thickness. Experimental test data for this N-155 thick-walled tube are reported in reference 1.

The circumferential strain rate on the outside of the tube governs the deformation or growth of the tube:

$$\dot{\epsilon}_{\theta b} = \frac{\sqrt{3}}{2} \dot{\epsilon}_b \quad (3)$$

The equivalent strain-rate  $\dot{\bar{\epsilon}}_b$  is related to the equivalent stress in equation (1):

$$\dot{\bar{\epsilon}}_b = A \bar{\sigma}_b^n e^{-\Delta H/RT}$$

Substituting, we obtain

$$\dot{\epsilon}_{\theta b} = \frac{\sqrt{3}}{2} A \bar{\sigma}_b^n e^{-\Delta H/RT}$$

Equation (5) (see ref. 2) gives the value for  $\bar{\sigma}_b$ :

$$\bar{\sigma}_b = \frac{\sqrt{3}}{n} \frac{1}{\rho^{2/n} - 1} p \quad (5)$$

Substitute for the independent variables in equation (5)

$$n = 3.310$$

$$p = 10.34 \text{ MN/m}^2 \text{ (1500 psi)}$$

$$\rho = 0.635 \text{ cm} / 0.391 \text{ cm} = 1.6234$$

Upon evaluation,

$$\bar{\sigma}_b = 15.91 \text{ MN/m}^2 \text{ (2308 psi)}$$

When values for constants from the correlation equation (2) are used in equation (4), the equation for  $\dot{\epsilon}_{\theta b}$  becomes

$$\dot{\epsilon}_{\theta b} = \frac{\sqrt{3}}{2} (7.517 \times 10^8) (15.91)^{3.310} e^{-396500/8.3143(1089)}$$

$$\dot{\epsilon}_{\theta b} = 0.59 \times 10^{-6} \text{ cm/(cm)(hr)}$$

This strain rate is less than  $1 \times 10^{-6} \text{ cm/(cm)(hr)}$ , which corresponds with 1-percent growth of the tube in 10 000 hr of operation. Therefore, step (2) of the design procedure is satisfied.

Strain rates are strongly temperature dependent. In figure 1, the temperature dependence is shown by the distance between the isothermal lines for 1214 and 1234 K. Go-

ing from 1214 to 1234 K at constant stress increases the strain rate by a factor of approximately 2.

The creep-rupture stress for the tube for design conditions is obtained from the graph in figure 4. The Larson-Miller parameter is calculated as

$$\left. \begin{aligned} P &= 1.8 T(\log t + 17.2) \times 10^{-3} \\ &= 1.8 (1089)(4.0 + 17.2) \times 10^{-3} \end{aligned} \right\} \quad (6)$$

$$P = 41.56$$

The value for ultimate stress may be read from the graph, or it may be calculated by fitting an equation to the solid line on the graph. The equation for the line is

$$\log \bar{\sigma}_u = 7.319281 - 0.137698 P \quad (7)$$

Substituting for  $P$  and solving result in

$$\bar{\sigma}_u = 39.54 \text{ MN/m}^2 (5.73 \text{ ksi})$$

This value for stress checks with a value of 39.5 read from the graph in figure 4.

The equivalent stress in the bore of the tube from helium pressure is correlated with the creep-rupture of the tube. The equation for that stress is (from ref. 2)

$$\bar{\sigma}_a = \frac{\sqrt{3}}{n} \frac{\rho^{2/n}}{\rho^{2/n} - 1} p \quad (8)$$

Substituting and solving yield

$$\bar{\sigma}_a = 21.32 \text{ MN/m}^2 (3.09 \text{ ksi})$$

This is the maximum equivalent stress in the tube for operating conditions.

The safety factor  $N_1$  based on creep-rupture becomes

$$\begin{aligned} N_1 &= \bar{\sigma}_u / \bar{\sigma}_a \\ &= 39.54 / 21.32 \\ &= 1.85 \end{aligned}$$

This value is greater than the 1.50 required for the design.

Another safety factor of interest is the ratio of the expected life at design temperature and pressure to the design life. The ultimate equivalent stress for the expected creep-rupture life with design helium internal pressure is given by equation (5), which becomes

$$\bar{\sigma}_u = \bar{\sigma}_a = \frac{\sqrt{3}}{n} \frac{\rho^{2/n}}{\rho^{2/n} - 1} p$$

$$\bar{\sigma}_u = 21.32 \text{ MN/m}^2 \text{ (3.09 ksi)}$$

Using this value for stress in equation (4) gives

$$P = 43.50$$

The expected life is obtained by substituting the values for  $P$  and  $T$  into equation (6):

$$t_u = 10^5 \text{ hr}$$

Finally, substituting values for expected life and design life into the equation for the lifetime safety factor  $N_2$ , we have

$$\begin{aligned} N_2 &= t_u/t_d \\ &= 10^5/10^4 \end{aligned}$$

$$N_2 = 10$$

The requirements for this heat-exchanger tube application are satisfied. Creep deformation during the 10 000-hr lifetime is the most limiting requirement. The expected growth of the diameter of a heat-exchanger tube during the full design lifetime is less than 1 percent.

The effects of vibrations, high-velocity air, and thermal stresses and the effects of thermal cycling from startup and shutdown of the engine are assumed to be included in the factor of safety  $N_1 = 1.50$ .

Thermal stresses in heat-exchanger tubes are initiated at startup of the engine. Since, however, the service temperature of the tubes is greater than  $0.5 T_m$ , creep will occur to relieve those stresses, and after a short period of operation, the primary mech-

anism of deformation, secondary creep, will develop the system of stresses and strain rates given in reference 2. These equations will apply until the engine is shut down. Then a reverse set of thermal stresses will be established. As the tubes cool, they will have higher strength than in their former hot condition. Residual stresses will remain and therefore reduce the level of thermal stresses set up when the engine is again started up.

The effects of thermal cycling and thermal fatigue from the repeated strain cycling caused by thermal stresses require further study.

These calculations show that welded N-155 heat-exchanger tubes can be designed for steady-state operation at temperatures greater than  $0.5 T_m$  ( $1089 \text{ K}$  ( $1500^\circ \text{ F}$ ) for this case) for lifetimes as great as 10 000 hr with an internal helium coolant pressure of  $10.34 \text{ MN/m}^2$  (1500 psi).

## CONCLUDING REMARKS

A correlation equation is presented for creep strain rates for thick-walled, welded, N-155 tubes tested under conditions of constant internal pressure and temperature. The ranges of data correlated include temperature, 1061 to 1234 K ( $1450^\circ$  to  $1760^\circ \text{ F}$ ); stress, 16.2 to  $77.6 \text{ MN/m}^2$  (2.35 to 11.25 ksi); ratio of tube outside to inside diameter, 1.154 and 1.623; strain rates,  $44.5 \times 10^{-6}$  to  $584.0 \times 10^{-6} \text{ cm}/(\text{cm})(\text{hr})$ ; and test time, 140 to 1857 hr.

Graphs of data verified that the strain-rate correlation equation obtained by using a multiple linear correlation analysis was representative of the experimental data. The correlation equation is useful for the interpolation and the extrapolation of the experimental N-155 thick-walled tube creep-rupture data for design applications requiring low creep strain rates at stresses less than  $40 \text{ MN/m}^2$  (5.8 ksi).

A procedure for the application of the correlation equation in the design of helium-to-air heat-exchanger tubes is presented. Design calculations showed that a welded, thick-walled, N-155 heat-exchanger tube having an outside diameter of 0.635 cm and an inside diameter of 0.391 cm was satisfactory for a 10 000-hr service life at  $1089 \text{ K}$  ( $1500^\circ \text{ F}$ ) with  $10.34 \text{ MN/m}^2$  (1500 psi) internal pressure and with a limitation of 1 percent on creep deformation during service life. The procedure presented for the analysis of welded N-155 heat-exchanger tube test data is applicable for the analysis of tube test strain-rate data obtained for other tube materials.

Lewis Research Center,  
National Aeronautics and Space Administration,  
Cleveland, Ohio, June 21, 1971,  
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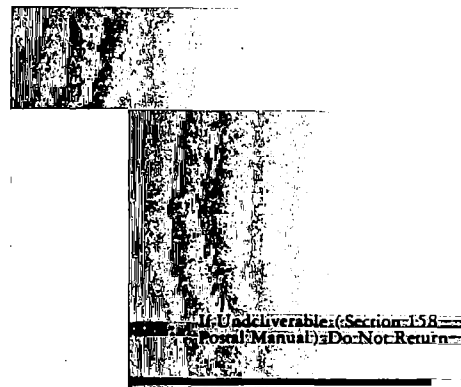


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